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MASS TRANSFER COOLING ON A POROUS FLAT PLATE IN CARBON-DIOXIDE AND AIR STREAMS

by

A. L. LAGANELLI[1]

J. P. HARTNETT[2]

Department of Mechanical and Aerospace Engineering
University of Delaware, Newark, Delaware

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1. Present address: General Electric Co., MSD
Thermodynamics U1217, King of Prussia, Pennsylvania.
Member AIAA

2. Professor and Head, Department of Energy Engineering, University of Illinois, Chicago Circle, Chicago, Illinois.
Member AIAA

NOMENCLATURE

c_f	skin-friction coefficient
c_p	specific heat
e_{12}	effective emissivity
$\bar{E}$	temperature effectiveness ratio, $(T_w-T_c)/(T_r-T_c)$
F	transpiration rate normal to surface, $(\rho v)_w/(\rho u)_1$
F_c	function multiplying c_f in universal drag law, $\left[\int_o^1 \left\{\frac{\rho/\rho_1}{1 + (\frac{\rho_w v_w u_1}{\tau_w})(\frac{u}{u_1})}\right\}^{\frac{1}{2}} d(\frac{u}{u_1})\right]^{-2}$
F_{RX}	function multiplying Re_x in universal drag law: $\dfrac{\frac{\mu_1}{\mu_w}\left[\frac{\rho_w}{\rho_1}(1 + \frac{\rho_w v_w u_1)}{\tau_w}\right]^{\frac{1}{2}}}{F_c}$
h	heat transfer coefficient, $q = h(T_w-T_r)$
k	thermal conductivity
ℓ	characteristic length, non-porous run of boundary layer growth
P	pressure
$(\Delta P)^2$	difference of square of pressure across porous plate (measure of porosity)
Pr	Prandtl number, $\mu C_p/k$
q	local heat-transfer to porous wall
r	recovery factor, $(T_w-T_c)/(T_r-T_c)$
R_e	Reynolds number: $\rho ux/\mu$, $\rho ux_E/\mu$

ABSTRACT

Heat transfer results are reported for a transpiration cooled porous flat plate placed in a stream of air, and of CO_2. The tests were performed at a Mach number of 1.96 over a range of effective length Reynolds number from 5 million to 9.1 million when CO_2 was used as the free stream gas. A Mach number of 2.53, for an effective length Reynolds number range of 5.3 million to 8.3 million, was characteristic when the free stream gas was air. The heat-transfer data were normalized and presented as the ratio of the Stanton number to the no-blowing Stanton value (St/St_o) as a function of the dimensionless transpiration rate F/St_o. The recovery factor data were also normalized and presented as the ratio of r/r_o as a function of the transpiration rate F.

The results for both the air, and the CO_2 free stream flows, showed a reduction in heat-transfer with increasing transpiration rate using air and CO_2 as the injectant gases. The measured recovery factor, and the normalized recovery factor, also decreased with increasing transpiration for the reported gas combinations. It was found that Rubesin's air theory adequately predicts all the heat-transfer results including those obtained in CO_2 atmospheres within the reported Mach number range. Also, the empirical theories which predict recovery factor results for air free streams can be used for air or CO_2 injection into a CO_2 free stream gas.

$\bar{S}$	thickness of porous plate
S_h	Reynolds analogy factor = $(Pr)^{2/3}$
St	local heat-transfer Stanton number, $h/\rho_1 u_1 C_{p_1}$
T	temperature
T_o	stagnation temperature
u, v	velocity
x, y, z	coordinate distances
x_E	characteristic distance
μ	viscosity of gas
ν	kinematic viscosity
ρ	density of gas

Subscripts

c	coolant or injection gas
0	zero blowing conditions
r	recovery conditions
w	porous wall surface conditions
1, ∞	free stream conditions
i	incompressible

INTRODUCTION

Developments in high thrust rocket engines during the past decade have made both manned and unmanned space travel a reality. As a result, more sophisticated high speed vehicle surfaces have been developed. Highly efficient methods of cooling have been developed, and it is now possible to design high speed vehicles to sustain the aerodynamic heating associated with hypersonic flight through the atmosphere or with re-entry into the earth's atmosphere (or entry into the atmosphere of another planet). These methods are also applicable for the cooling of combustion-chamber walls, rocket-motor nozzles, and gas turbine blades.

One of the most effective processes for cooling such high performance surfaces is that of mass transfer cooling which involves the injection of a foreign gas (or liquid) into the boundary layer. A detailed description concerning specific examples of the mass transfer cooling process can be found in reference (1). The method of particular interest in this investigation is that of transpiration cooling which involves the injection of a coolant gas through a porous structure into the surrounding high temperature boundary layer. The mass injected through the porous surface reduces the heat flux in the following ways: (a) it modifies the velocity distribution, decreasing the frictional heating and thickening the boundary layer, and (b) it provides a heat sink, absorbing some of the heat flux which would otherwise get to the surface.

References (2, 3, 4) represent a fairly comprehensive review up to September 1960 on the effects of gas injection and heat transfer into a compressible turbulent boundary layer. Some of the more recent experimental and analytical results concerning the heat transfer and recovery factors in a turbulent compressible boundary layer will be briefly mentioned.

ANALYTICAL RESULTS

The study of the turbulent boundary layer with mass transfer requires assumptions related to the turbulent structure which are often vague and incomplete. The existing knowledge in the subject leaves considerable uncertainty as to which of the several theories gives the best prediction. This uncertainty arises since each theory contains many simplifications which have not been adequately supported by the limited experimental data.

The most popular of the existing theories are found in the papers by Rubesin (5), and Dorrance and Dore (6). Dorrance has now formulated much of his work in a published book (7). The above papers were concerned with mass transfer in a turbulent boundary layer for air injection into an air free stream. Rubesin and Pappas (8) extended the earlier work of Rubesin and accounted for foreign gas injection into an air free stream. More recently, the work of Kutateladze and Leont'ev (9), Spalding, Auslander, and Sundaram (10), and Leont'ev (11) have contributed to this field. References (10, 12) have reported the various contributions in a tabular form, listing the basic assumptions associated with each study.

EXPERIMENTAL RESULTS

It appears that all of the experimental work concerning the effects of gas injection into a turbulent boundary layer have been conducted with air as the free stream gas with the exception of limited data obtained in rocket exhausts [see, for example, Brunner, (13)]. Although the objectives of the various studies are quite similar, the heat transfer models and the testing equipment varied extensively.

Some of the earlier and more pertinent papers in the field are found in references (14, 15, 4). More recently, Bartle and Leadon (16), Tewfik, Eckert, and Jurewicz (17), Tewfik, Eckert, and Shirtliffe (18), Pappas and Okuno (19), and Brunner (13) have obtained heat transfer measurements on various geometrical models. The results are usually presented as a ratio of the Stanton number to the no-blowing Stanton number St/St_o as a function of the transpiration rate F/St_o.

In summarizing the results for air injection into an air boundary layer, the experimental heat transfer measurements for a Mach number range of 0 to 4.0 are in good agreement with Rubesin's theory (5). Dorrance and Dore's theory (6) predicts heat transfer results that are somewhat low when compared to experimental measurements. By its very nature, the Spalding et al theory (10) is in agreement with experimental results inasmuch as the correlation was developed from existing experimental data. However, for foreign gas injection, the low speed theory developed by Rubesin and Pappas (8) was not in agreement when compared to experimental data. The reader is referred to reference (12) for a more detailed discussion of the above mentioned papers.

DESCRIPTION OF EQUIPMENT

The experimental program was conducted in an Aerolab 1" x 1" Supersonic Wind Tunnel of the intermittent blow-down type. The asymmetric nozzle was composed basically of curved upper and lower walls with flat side walls. The heat-transfer model, which was part of the test section wall, consisted of a plenum chamber where the porous plate served as the bottom wall in the chamber and the top wall of the tunnel test Section (see Figure 1).

The porous plate was 1-1/2" x 5" long with an average surface thickness of 0.050 inch. It was made up of a sintered type 315-L stainless steel PSS GRADE H material, fabricated by the Pall Trinity Micro Corporation. A measure of the surface contour showed an average roughness height of ± 150 microinches. The density of the porous material was 0.17 pounds per cubic inch and had a mean pore opening of 5 microns. The void content was approximately 45%. The test surface porosity distribution was measured and the results are presented in Figure 2. The effective porous plate area, that area of the plate exposed to the free stream flow, was 0.0345 square feet.

In considering the secondary flow system, extreme care was taken to obtain an even flow distribution over the porous plate surface. Two 1/4 inch diameter tubes were used for the secondary gas permitting its passage into a flow distributor. Holes were drilled in symmetrical fashion along the bottom surface of the oblong shaped distributor, the holes increasing in size as they proceeded away from the inlet tubes. The flow was further distributed by two 36 x 36 mesh per square inch screen and a metal gauze-like screen. The screens were made of copper and were located 1/4 inch below the oblong distributor. The gas then passed through 8 layers of fiberglas filter paper manufactured by the Mine Safety Appliance Company, Type 1106-B. The filter paper served the useful function of providing a uniform and controlling resistance for the secondary flow, thereby minimizing any non-uniformities in the injection system.

Thermocouples, fabricated from 36 gauge (0.005" diameter) copper-constantan nylon insulated wires, were positioned at eight locations along the plate. Four were positioned along the center line, one inch apart, starting one inch from the leading edge. The remaining four were located on each side of the center line and 3/8" from it. A 0.020 diameter hole 0.040 inch deep was drilled into the porous plate. The thermocouple bead was inserted into the hole and cemented into place with copper-oxide cement. Four additional thermocouples, also fabricated from 36 gauge copper-constantan wire, were located between the seventh and eighth fiberglas filter layers lying above the porous plate for measuring the coolant temperatures. These were located directly above the center line locations of the porous plate thermocouples.

The thermocouples were connected to a Leeds and Northrup rotary selector switch which was driven by a 20 r.p.m. motor. The leads were then connected to an ice bath and a Honeywell Model 206 Visicorder Oscillograph. The secondary flow rate was measured by a calibrated Rockwell Model 250 gas meter. The gas was heated in a unit that contained five Nichrome wire grids, each grid consisting of six wires. The grids were controlled by a 115 volt, 15 ampere powerstat. The meter, stagnation, and static pressures were continuously recorded on a Bristol Model 500 pressure recorder. Since the length of a constant Mach number test was of the order of 24 seconds, it required continuous recording of all pertinent data from a synchronized starting point.

EXPERIMENTAL PROCEDURE AND DATA REDUCTION

Several tests were performed to calibrate the wind tunnel and the measuring equipment. Schlieren tests were made at the intersection of the heat transfer model and the test section for the detection of possible shock formations due to surface discontinuity. The heated secondary gas was injected through the porous flat plate into the free stream--the opposite of the situation encountered in actual practice. If the investigation is restricted to small temperature differences between the porous wall and the free stream, the heat transfer measurements may be applied to the situation where the transpiration gas is colder than the free stream flow.

The transpiration gas was set for the desired flow rate and temperature level. Upon establishing steady state conditions the following readings were recorded:

(a) coolant gas temperature
(b) coolant gas pressure
(c) volumetric flow rate of the coolant gas
(d) wall temperatures of the porous plate
(e) wind tunnel conditions
  (i) stagnation pressure
  (ii) static pressure of test section
  (iii) stagnation temperature
(f) barometric pressure and temperature

A series of tests were made at various values of the dimensionless mass flow rate F and coolant temperature levels. A heat and mass balance across an elemental area of the porous plate establishes an equation to determine the local heat transfer coefficient from the measured data. Consider an element of the porous plate as shown in Figure 3. The mass of the coolant flows across the area dx · dy and height dz with a temperature difference between the porous surface and the gas stream of the coolant. For a steady-state situation, a heat balance can be expressed as:

$$q_{coolant} = q_{convected} + q_{conducted} + q_{radiated} \tag{1}$$

Estimates showed that the conduction term is less than 10% of the convection term for dimensionless flow rates, F, greater than 0.15%, while the radiation term is negligible over the complete range of the investigation (12). Accordingly, the last two terms of Eq. (1) were neglected in the analysis of data.

Assuming constant specific heat, the heat absorbed (or released) by the coolant may be represented as:

$$q_{coolant} = (\rho v)_w \, c_{p_c} (T_w - T_c) \tag{2}$$

The flat plate model of Schneider (20) was used to estimate the temperature drop across the porous plate and was shown to be less than 1%, allowing the use of the measured inner wall temperature for the outside wall temperature. The term $(\rho v)_w$ in Eq. (2) is found by employing the measured volumetric flow rate (Q), meter temperature, meter pressure and the perfect gas equation. Since T_c and T_w are also measured values, Eq. (2) can then be easily solved.

The heat entering the plate by convection is the heat lost through conduction into the gas stream at the surface z = 0. This can be expressed as:

$$q_{convected} = k \left. \frac{\partial T}{\partial z} \right|_{z=0} = h(T_r - T_w) \tag{3}$$

where T_r (the recovery temperature) is that particular temperature of the porous wall and the gas leaving the wall, such that the temperature gradient at the wall is zero.

Neglecting conduction and radiation and substituting Eqs. (2) and (3) into Eq. (1):

$$q = h(T_w - T_r) = (\rho v)_w \, c_{p_c} \, (T_c - T_w) \tag{4}$$

At a fixed value of the injectant flow rate $(\rho v)_w$, the coolant temperature (T_c) was set at several different temperature levels and the resulting wall temperature T_w was measured. The flow rate was then set at a different value and the process was repeated. The heat-transfer coefficient, h, in Eq. (4) is evaluated for each fixed value of the blowing rate from a graphical plot of q versus the wall temperature for each surface temperature location. This is possible inasmuch as the coolant temperature is changed while maintaining a constant Mach number and local Reynolds number. Because of the linear nature of Eq. (4), a straight line can be expected, with h being the slope of the line and T_r the intercept value along the $q = 0$ axis. In the actual analysis of data, curves of q/T_o vs. T_w/T_o were utilized in an attempt to account for variations in the stagnation temperature, T_o.

DISCUSSION OF RESULTS

An accurate value of the no-blowing Stanton number St_o is desired to normalize the heat-transfer coefficient ratio for the purpose of comparing data of this report to other published literature. A widely used method to determine the St_o value is to extrapolate the curve of the local Stanton number data as a function of the injection rate to the value at zero blowing. A linear equation was assumed and a least squares analysis was applied to determine the St_o value by extrapolating the data to the zero injection value. However, as a result of scatter in the data an alternate method was chosen in an attempt to improve the reliability of the St_o value. In particular, the well-known Colburn type equation, which applies to low-speed flow, was used as a basis for determining the zero blowing Stanton number; this incompressible Stanton number was then corrected to account for the unheated length and for the Mach number. The low-speed Stanton number can be written as:

$$St_{oi} = \frac{0.0296}{(Re_x)^{0.2}(Pr)^{2/3}} \tag{5}$$

Equation (5) is corrected to account for the discontinuity due to an unheated starting length. Using the Seban formula (21, 22), we write:

$$St_{oi} = \frac{0.0276\left[1 - \left(\ell/x_E\right)^{9/10}\right]^{-1/9}}{(Pr)^{8/9}(Re_{x_E})^{1/5}} \tag{6}$$

Here, ℓ is the distance from the effective start of turbulence to the leading edge of the porous heated plate, and x_E is the distance from the effective start of turbulence to the local thermocouple position.

The Seban formula, Eq. (6), has been verified experimentally by Eichhorn et al, (23) and, furthermore, over the range of the present studies it is in agreement with the recent analysis of Spalding (26).

It may be seen from Eq. (6) that the distance from the effective start of turbulence to the leading edge of the porous plate must be accurately known. The liquid film (25) test was used to determine the effective start of turbulence along the tunnel wall. Several tests were performed, and it was concluded that the effective start of turbulence was at the throat of the nozzle blocks. The measured Mach number coupled with the analysis of Sibulkin (26) supported this conclusion (12). With the knowledge of the effective starting length, the incompressible zero blowing Stanton number, Eq. (6), with the Seban correction for the discontinuity due to an unheated starting length can now be determined. Since these calculated values are valid for low-speed flow, adjustments are made for the actual Mach number (2.53 for the air free stream and 1.96 for the CO_2 free stream) using the empirical curve of reference (27). These predicted values of the no-blowing Stanton number using Eq. (6) and reference (27), were compared with the extrapolated measured values. The extrapolated air result was 11% lower than the calculated value. It was felt that the predicted values of the zero blowing Stanton number were somewhat more accurate than the extrapolated measured values and accordingly, the predicted results are used throughout in normalizing the heat transfer.

The experimentally determined heat transfer and recovery factor results are shown in Figures 4 and 5. In particular, Figure 4 reveals the normalized Stanton numbers, St/St_o, for air and carbon-dioxide injection into free streams of air or carbon-dioxide at a nominal Mach number of 2 as a function of the dimensionless blowing rate, F/St_o. It may be seen that all experimental Stanton results are in fair agreement with the widely used air-to-air theory of Rubesin. A detailed inspection of the air-to-air experimental results would show that they are in good agreement with the earlier reported results of Rubesin et al, (14), Leadon and Scott (15), Pappas and Okuno (19), Bartle and Leadon (16), Tewfik et al, (17), and Romanenko and Kharchenko, (28).

The measured recovery factors are shown on a normalized basis, r/r_o, as a function of the blowing rate F on Figure 5. The solid wall recovery factor values, r_o, were determined by extrapolating the measured values to zero blowing. This procedure yielded a value of 0.94 for the air free stream, approximately 4% higher than the expected value of 0.90.

An extrapolated recovery value of 0.97 was found for the carbon-dioxide free strea, some 5% higher than the value expected from the predicted value (i.e., $r_o = Pr^{1/3}$). Considering the nature of the experimental program, a transient testing procedure with a small test section, this agreement is considered satisfactory. A single curve is seen from Figure 5 to represent the normalized recovery factor for all the air and carbon-dioxide tests.

The temperature effectiveness ratio as a function of the dimensionless blowing rate $Fc_{p_c}/St_o c_{p_1}$ is shown on Figure 6 for both the air and carbon-dioxide free streams. Also shown is the curve proposed by Bartle and Leadon (16) for an air free stream. It is seen that the empirical correlation of reference (16) is not only in very good agreement with the air results, as would be expected, but also agrees very well with the measurements obtained with a carbon-dioxide free stream.

A final representation, that suggested by Spalding et al (10), is shown in Figure 7, bringing out the agreement of the air free stream results with those found for a carbon-dioxide free stream. The present results are somewhat lower than the curve recommended by Spalding et al.

CONCLUSION

The injection of carbon-dioxide or air into an air free stream at a nominal Mach number of 2 give values of St/St_o vs. F/St_o which are in good agreement with values obtained for carbon-dioxide or air injection into a carbon-dioxide free stream and all values are in fair agreement with the air-to-air theory of Rubesin.

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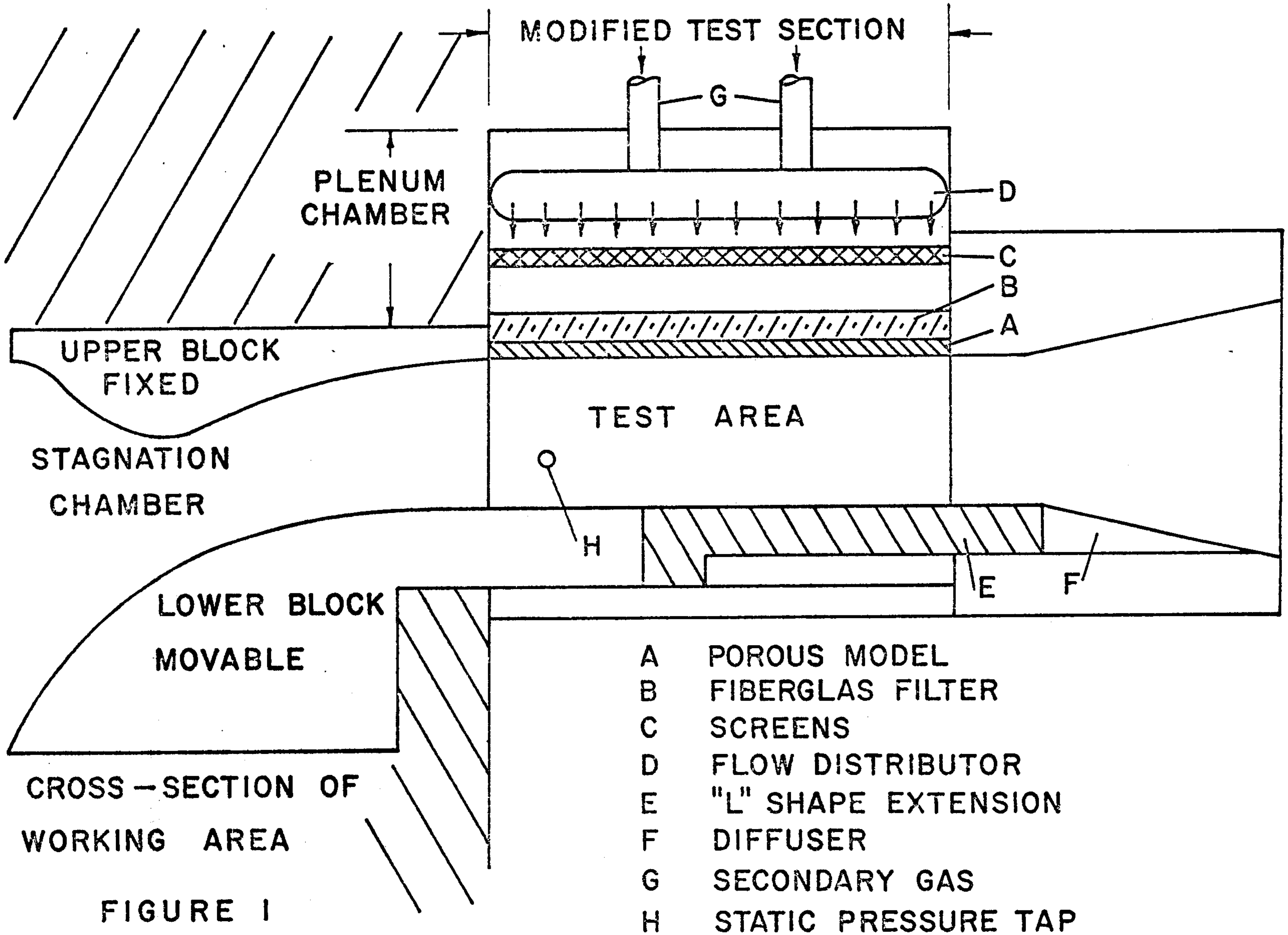
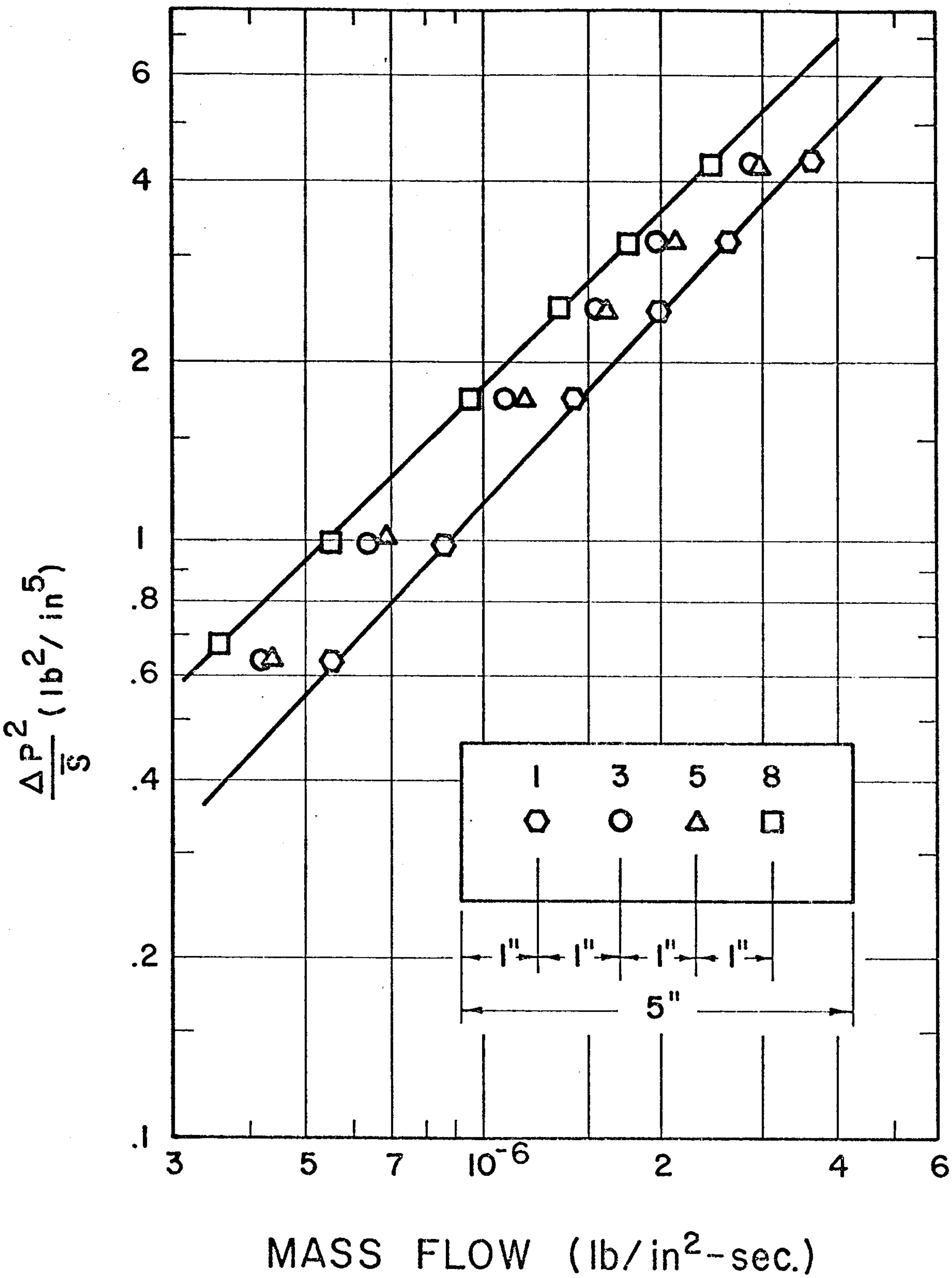


CROSS — SECTION OF WORKING AREA

FIGURE I



6
4
2
1
.8
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5
7
10^{-6}
2
4
6
1 3 5 8
1" 1" 1" 1"
5"
$\frac{\Delta P^2}{S}$ (lb^2/in^5)
MASS FLOW (lb/in^2-sec.)

FIGURE 2

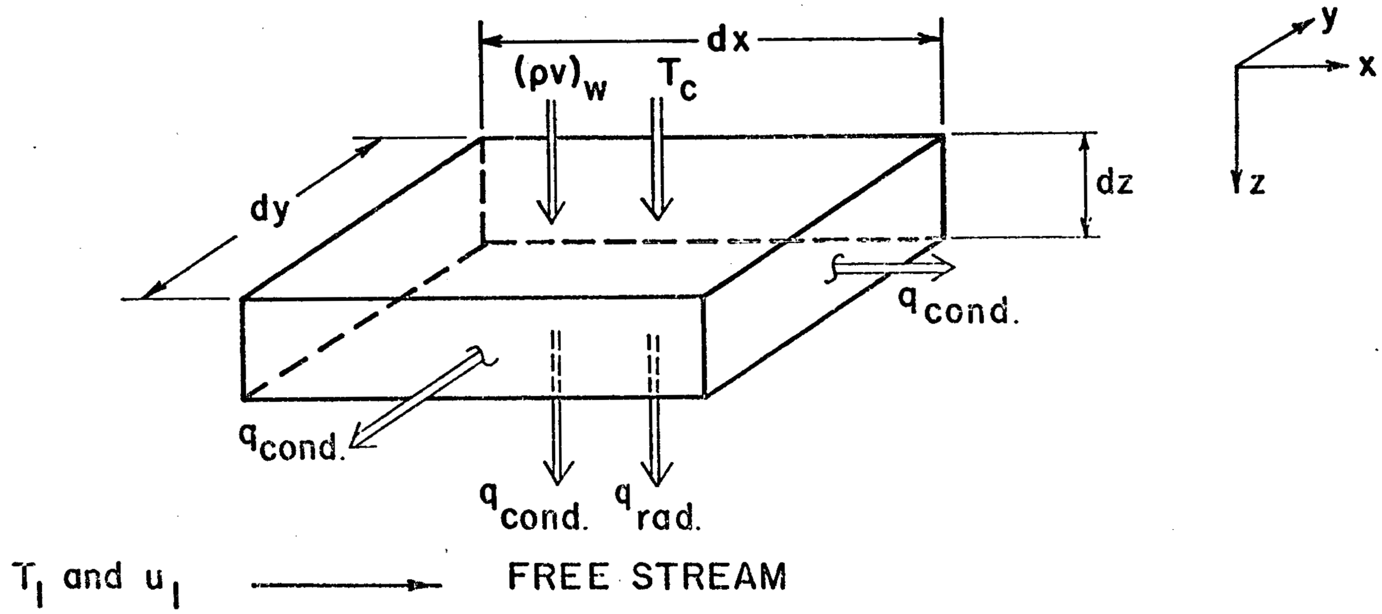

FIGURE 3

ELEMENT OF POROUS AREA

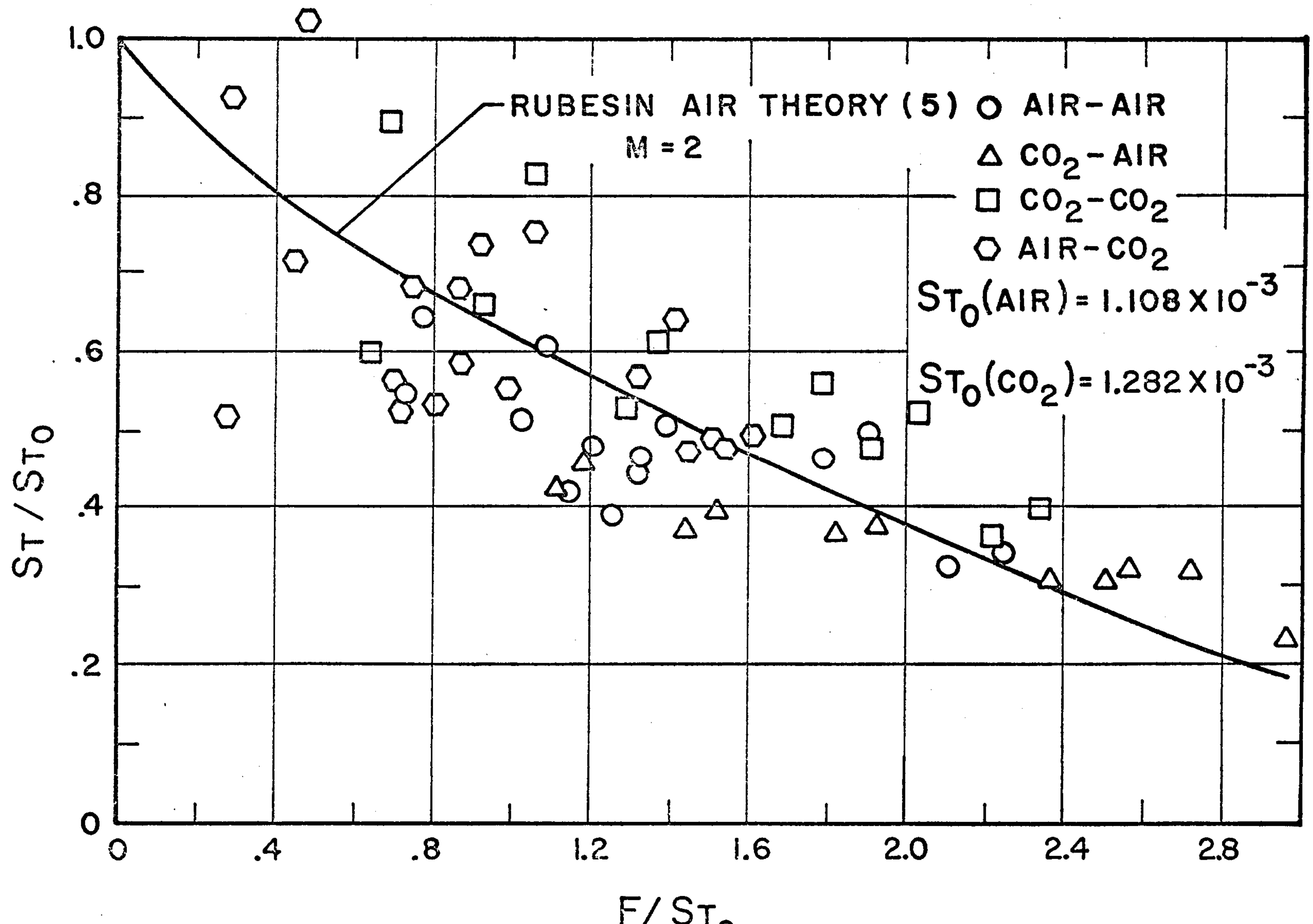

FIG. 4 Rubesin's Air Theory Compared to the Normalized Stanton Numbers for Various Gas Combinations.

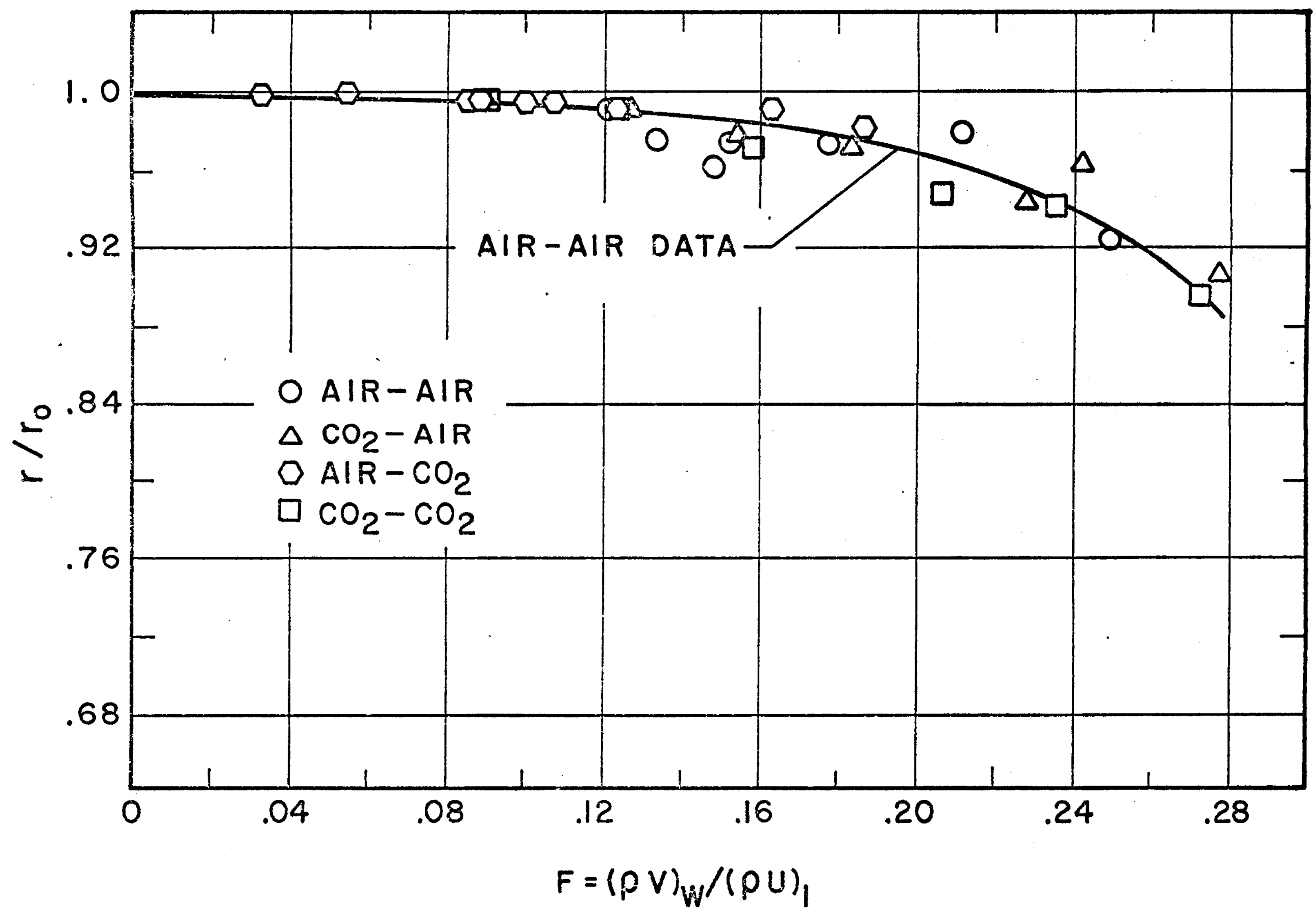

FIG. 5 Comparison of the Normalized Recovery Factors.

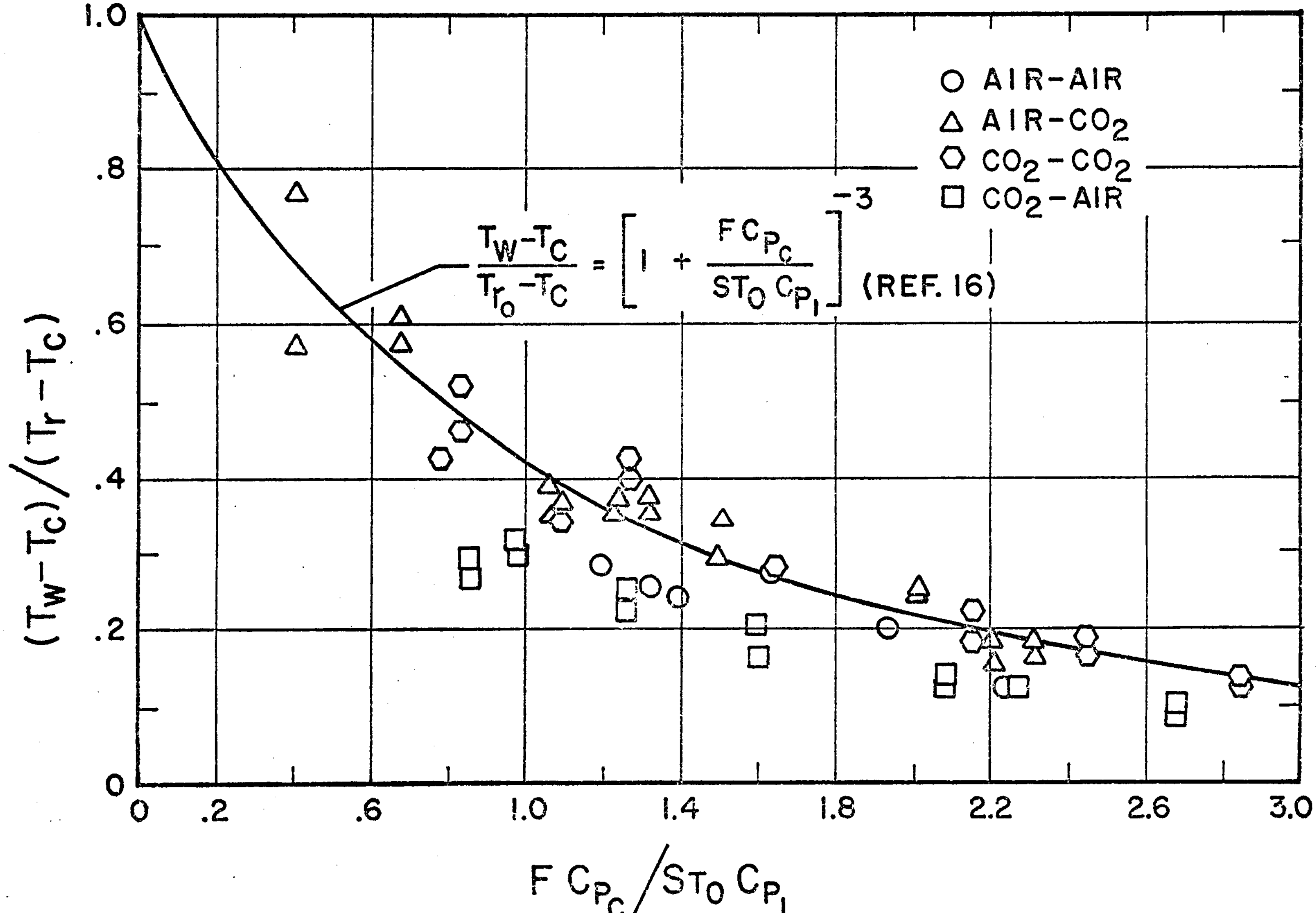

FIG. 6 Effectiveness Correlation for Transpiration Cooling.